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SSME Fuelside Preburner Two-Dimensional Analysis

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SUMMARY

The flow field within the fuelside preburner of the Space Shuttle Main Engine is calculated using a reacting flow code (REACT2D). Inlet and modeling parameters involved in the numerical calculation are systematically varied to establish the sensitivity of the calculated exit temperature profile. It is found that differences in the inlet equivalence ratio have a large effect on the turbine inlet temperature profile. A variety of preburner inlet modeling changes such as inlet turbulence level, modeling the gases as burned, unburned, premixed, or unmixed, are shown to have a smaller effect on the calculated turbine inlet temperature profile. Also, the form of finite differencing used is shown to have an effect on the temperature profile.

INTRODUCTION

The durability of the Space Shuttle Main Engine (SSME) can be a significant factor in establishing total launch costs of the Space Transportation System (STS), commonly referred to as the Space Shuttle. Frequent replacements of the engine or engine system components can potentially make the STS less cost effective than an expendable launcher. To reduce costs, NASA is sponsoring an effort to increase the durability of future SSME designs. This study is part of that effort.

The SSME design allows for a wider power range than previous rocket engines. This design calls for highly fuel rich or oxygen rich combustion in preburners to power the turbines that drive high pressure liquid oxygen or liquid hydrogen pumps and combustion is completed in a separate main combustion chamber. Due to flow losses and requirements of increased payload, the power level of the SSME has had to be increased. This has caused more frequent inspections and replacement of engines or engine components. One component that has experienced shorter life is the fuelside turbine which powers the high pressure liquid hydrogen pump. Durability of this turbine is strongly affected by the temperature profile leaving the fuelside preburner. This temperature profile, though not accurately known, is needed to design more efficient turbine blading and to make turbine blade life predictions. Experimental measurements of this temperature profile are difficult to make due to the severe operating environment within the SSME. Making measurements inside the SSME would be expensive, time consuming, and could compromise the integrity of the engine. Therefore, with the recent advances in computational capabilities, an easier design approach is to use a reacting flow computer code to predict the Turbine Inlet Temperature Profile (TITP). This was the objective of the current study. A two-dimensional code, REACT2D, was modified to analyze a preburner geometry with a hydrogen-oxygen single step reaction. A series of calculations were made to assess the sensitivity of the turbine inlet temperature profile to variations in the flow entering the SSME preburner.

SYMBOL LIST

$C_{p,i}$	specific heat for the ith component, kJ/(kg - K)
$C_{p,m}$	mean specific heat for the mixture, kJ/(kg - K)
C_μ	turbulence model constant
f	mixture fraction
H_R	heat of reaction, J/kg
h	enthalpy, J/kg
k	turbulent kinetic energy, m^2/s^2
M_i	molar mass of ith species, kg/mole
m_f	mass fraction of fuel
m_i	mass fraction of ith species
p	pressure, N/m^2
R	universal gas constant, 8314.3 J/(kg - mol K)
R_f	rate of combustion of fuel, $kg/(s - m^3)$
r	radial distance, m
S_u	source term for axial velocity, $kg/(m^2 - s^2)$
S_v	source term for radial velocity, $kg/(m^2 - s^2)$
S_φ	source term for the variable, $(kg/(m^3 - s))$*(units of φ)
T	temperature, K
u	axial velocity, m/s
v	radial velocity, m/s
x	axial distance, m
ε	turbulence energy dissipation rate, m^2/s^2
ρ	density, kg/m^3
μ_{eff}	effective viscosity: sum of laminar and turbulent viscosity, Ns/m
μ_ℓ	laminar viscosity, Ns/m^2
μ_t	turbulent viscosity, Ns/m^2
Γ_{eff}	the effective exchange coefficient for the variable φ, Ns/m^2

MATHEMATICAL FORMULATION

The time averaged Navier Stokes (or Reynolds) equations for two-dimensional axisymmetric flow are shown below (for details see ref. 1).

Continuity:

$$\frac{\partial}{\partial x}(r\rho u) + \frac{\partial}{\partial r}(r\rho v) = 0$$

Axial momentum:

$$\frac{1}{r}\left[\frac{\partial}{\partial x}(r\rho uu) + \frac{\partial}{\partial r}(r\rho vu)\right] = -\frac{\partial p}{\partial x} + \frac{1}{r}\left[\frac{\partial}{\partial r}\left(r\mu_{eff}\frac{\partial u}{\partial x}\right) + \frac{\partial}{\partial r}\left(r\mu_{eff}\frac{\partial u}{\partial r}\right)\right] + S_u$$

Radial momentum:

$$\frac{1}{r}\left[\frac{\partial}{\partial x}(r\rho uv) + \frac{\partial}{\partial r}(r\rho vv)\right] = -\frac{\partial p}{\partial r} + \frac{1}{r}\left[\frac{\partial}{\partial x}\left(r\mu_{eff}\frac{\partial v}{\partial x}\right) + \frac{\partial}{\partial r}\left(r\mu_{eff}\frac{\partial v}{\partial r}\right)\right] - \mu_{eff}\frac{v}{r^2} + S_v$$

μ_{eff} *is the sum of the laminar and turbulent viscosities.*

Scalar transport:

$$\frac{1}{r}\left[\frac{\partial}{\partial x}(r\rho u\varphi) + \frac{\partial}{\partial r}(r\rho v\varphi)\right] = \frac{1}{r}\left[\frac{\partial}{\partial x}\left(r\Gamma_{eff}\frac{\partial \varphi}{\partial x}\right) + \frac{\partial}{\partial r}\left(r\Gamma_{eff}\frac{\partial \varphi}{\partial r}\right)\right] + S_\varphi$$

Γ_{eff} is the effective exchange coefficient for the variable φ.

The turbulent viscosity is:

$$\mu_t = \rho C_\mu \frac{k^2}{\varepsilon}$$

Where k, the turbulent kinetic energy and ε, the dissipation rate of kinetic energy are solved for using scalar equations.

All but one of the reported calculations used hybrid differencing (ref. 1). Hybrid differencing is a combination of central and upwind differencing. When the cell Peclet number (the ratio of convection coefficient to the diffusion coefficient) is less than two, central differencing is used. At higher Peclet numbers upwind differencing is used.

One calculation was done using Bounded Skew Upwind Differencing (BSUDS). While hybrid differencing uses a five point computation molecule, the more accurate BSUDS scheme employs a nine point computational molecule to more accurately calculate convection terms when the flow direction is skewed with respect to the grid system (ref. 1). The scheme also uses a bounding routine to eliminate nonphysical oscillations or "wiggles" in the flow variables.

The prechamber walls are modeled as adiabatic and law of the wall is used.

COMBUSTION MODEL

The stoichiometric reaction is:

$$H_2 + \frac{1}{2} O_2 \longrightarrow H_2O + 1.21 \times 10^8 \frac{J}{kg}$$

The enthalpy was defined as:

$$h = m_f H_R + C_{p,m} T$$

The mean specific heat was given by:

$$C_{p,m} = \sum m_i C_{pi}$$

The specific heat for each component was approximated by a fourth order polynomial. The coefficients for the polynomial were calculated with a program by Mcbride and Gordon (ref. 2) using data from the Janaf tables, ref. 3. Two sets of coefficients are used for each species, one for a temperature range of 100 to 1000 K, and another for 1000 to 3000 K to obtain better accuracy. These coefficients are given in table I.

The eddy breakup combustion model of Magnussen and Hjertager (ref. 4) is used. This model relates the combustion rate to the rate of dissipation of turbulent kinetic energy and concentration of species. Combustion is modeled as a negative source term for the fuel mass fraction, m_f. The Magnussen-Hjertager model was developed for both premixed and diffusion combustion, unlike previous models which were developed for only one type of combustion. In a diffusion type flame the oxidant and fuel exist in separate eddies, which must mix before combustion can occur. The time averaged reaction rate is limited by the fuel concentration for lean flames, or by the oxygen concentration for rich flames. In premixed flames the fuel and oxidant occur in the same eddies and the reaction rate is determined by the flame spread, which is related to the concentration of product. The Magnussen-Hjertager combustion model takes the limiting concentration of fuel, oxidant, or product and calculates a negative source term for the mass fraction of fuel.

This source term is:

$$Sm_F = -\rho \frac{\varepsilon}{k} \min \left[Am_F, A \frac{m_{O2}}{STOIC}, AB \frac{m_{pr}}{STOIC + 1} \right]$$

A and B are empirical reaction constants and ε/k is the reciprocal of eddy lifetime. The present calculations used $A = 32$ and $B = 4$ unless noted otherwise. The quantities in the denominators are from the relations between product and reactants in the stoichiometric reaction.

The rate of combustion is then:

$$R_f = -S_{mf}$$

The combustion source terms were not calculated for initial iterations to allow the computational flow field to be established. If this was not done, the solution could have diverged. As can be seen from the above equation for the combustion source term, no reaction will occur if the product concentration is zero. The present program handled this by introducing some product at a

specified iteration in a specified cell or column of cells. Also, calculated combustion source terms could be so high that combustion was completed beyond what was physically possible. To correct this, the code was modified to check for and correct for these unrealistically large source terms.

The species concentrations were calculated by using two scalar variables, the fuel mass fraction and the mixture fraction (f and mf). The mixture fraction was defined as the total mass fraction of fuel, both burned and unburned.

$$f = m_f + \frac{2}{18} m_{H_2O}$$

The program calculates the fuel mass fraction and mixture fraction each iteration. Using these two variables other mass fractions may be found.

The mass fraction of product is:

$$m_{H_2O} = 9(f - m_f)$$

As there were only three species, the concentration of oxygen was:

$$m_{O_2} = 1.0 - m_{H_2O} - m_f$$

For this report, all the oxygen was consumed in the flowfield unless a low value of A and B were used in the reaction model.

The temperature may be found by manipulating the equation for enthalpy.

$$T = \left(\frac{h - m_f H_R}{C_{p,m}} \right)$$

The density was calculated using the equation of state for an ideal gas mixture.

GEOMETRY

The SSME configuration is shown in figure 1. The fuel preburner is located in the upper left side of the figure. The inlet to the preburner, or preburner faceplate, is three-dimensional. A section of the faceplate is shown in figure 2 (part of figure is from ref. 5). The major portion of the flow is through 264 oxygen and hydrogen coaxial injectors. There are also a number of very small hydrogen inlets used for area cooling. The computational grid used for these calculations is shown in figure 3. Seventy axial and 53 radial gridpoints were used. The preburner is modeled as a series of coaxial nozzles injecting into a chamber. The walls are approximated by a rectangular mesh. The cooling flow which enters through the outer prechamber liner in the actual SSME was included in the flow nearest the outer wall. The density of radial gridpoints was increased near the outer radius to ensure an adequate number of radial gridpoints in the exit flow. This grid choice required CPU times of about 5 min on the Cray 1S. The TITP was computed at an axial distance of

0.304 m, but the calculations were performed up to 0.5 m to insure that exit boundary conditions did not affect the TITP.

The inlet conditions were calculated from data provided by Rocketdyne. These are listed in table II. The inlet was divided into five zones by Rocketdyne to calculate approximate temperature profiles. These zones were the ignitor zone (in the center) and lettered zones corresponding to circular rows of inlet nozzles. There are eight rows of nozzles corresponding to the letters A through H. Zone A-B is next to the igniter, followed by zones C-D, E-F-G, and H. Zone H is the outermost zone and it included the liner cooling flow. Inlet conditions used for the first calculation are listed in table III. Using a zero or very small inlet velocity for the inlet zone caused the program to diverge, so a nonzero inlet condition was used for the igniter. This flow was less than 0.6 percent of the total flow. Changing this flow slightly did not affect the TITP. However if a kinetic reaction model were used, this might not be true.

Originally, plans were made to run a vectorized three-dimensional code, after two-dimensional calculations, but the three-dimensional code has not been successfully vectorized.

All of the two-dimensional calculations modeled the prechamber inlet as coaxial inlet zones. No walls were modeled in the injector plane. To model wall surfaces between injectors would have involved much more work in modifying the code. The prechamber inlet has wall surface surrounding each injector, as shown in figure 2. These surfaces cause a large velocity gradient which promotes higher turbulence. Higher turbulence could effect the calculated TITP.

The outer wall and prechamber dome were modeled as being adiabatic. All the calculations presented here required modifying these inlet conditions or using combinations of them.

RESULTS

Baseline Calculation

Using the inlet conditions listed in table III, a baseline calculation was made. This calculation presents the closest approximation to the preburner design studied in this report. It is provided as a baseline against which parametric variations can be compared.

The TITP and velocity vector diagram for the baseline calculation are shown in figures 4(b) and (c). The temperature profile shows much higher temperatures around $r = 0.113$ m. The TITP is within the range of temperatures listed in table IV, which lists final temperatures for some mixtures. Also shown in figure 4(b) is a TITP from a Rocketdyne calculation. The velocity vector diagram does not show any recirculation. Combustion is very rapid for this calculation. The combustion causes the velocity to increase by a factor of three within a few grid points which helps increase the level of turbulence. Velocity only increases again due to a reduction in flow area.

Modified Inlet Conditions

The first modification was to change the inlet conditions to determine the effect on the TITP. The ignitor inlet condition for the baseline calculation was a mixture of combustion gases at a temperature of 1000 K. A second calculation with a cold (i.e., unreacted) ignitor flow was made and the TITP was found to be similar. Another calculation which modeled the inlet gases as hot combustion products at the appropriate velocity and temperature was made. This calculation also gave the same TITP as the baseline calculation. However this calculation did show a small recirculation zone at the center of the preburner, on the prechamber dome (fig. 5). This series of calculations illustrate that the inlet conditions assumed for the igniter can significantly affect the calculated flow field. Despite this effect, the TITP was insensitive to these changes. Apparently the ignitor represents such a small fraction of the entire flow that it does not significantly affect the exit temperature.

Equivalence Ratio Changes

A series of calculations in which the equivalence ratios of the inlet zones noted in figure 4(a) were varied. Changing the inlet conditions for the ignitor zone did not effect the TITP. The long dashed line corresponding to changing the ignitor zone inlet condition to that of zone A-B plotted on top of the TITP for the baseline (fig. 6(b)). The moderately long dashed line in figures 6(a) and (b) shows the affect of lowering the equivalence ratio of zone A-B and the igniter to that of zone C-D. The temperature profile was only moderately affected in the radial areas up to $r = 0.112$ m. Note, that the temperature is increased by lowering the equivalence ratio because the equivalence ratio is so high. Changing the inlet condition of zone H to that of zone E-F-G only affected the outer portion of the TITP (short dashed line). However, changing the inlet condition of zone E-F-G to that of zone H had an effect over the whole TITP (see figs. 7(a) and (b)). Since inlet zone E-F-G accounts for almost half of the total flow this result should be expected.

By using different inlet conditions TITPs that were totally skewed in one direction could be generated. Using a set of inlet conditions with decreasing equivalence ratios resulted in a TITP with temperature increasing from the inner wall to the outer wall (fig. 8(b)). Keeping the overall mass flow similar, a series of inlet conditions were found with increasing equivalence ratio, which yielded a TITP that was skewed in the opposite direction.

Diffusion Combustion Calculation

All of the previous calculations used inlet conditions in which the oxygen and hydrogen were premixed for each zone. In the preburner, the flow is not premixed. The oxygen and hydrogen flow from separate nozzles. To model separate oxygen and hydrogen nozzles the number of inlet conditions was increased (see table V). The mass flow rates of oxygen and hydrogen for each zone was kept the same as the baseline calculation. Also, the inner region of zone A-B was approximated by a slower speed hydrogen nozzle to better model the actual geometry (fig. 2). The ignitor zone used the baseline inlet condition (mixed product and fuel at 1000 K). Temperatures vary widely downstream of the inlet nozzles due to the extreme variation in local equivalence ratio. For example, a temperature profile at $x = 0.153$ is shown in figure 9(a). Despite this the

TITP was very similar to the baseline calculation with only a minor variation in the outer radial portion of the TITP (fig. 9(b)). The temperature profile further downstream for this case came even closer to matching the baseline TITP. A color coded temperature plot for this calculation is shown in fig. 9(c). This figure shows intense mixing occurring near the rear wall of the preburner.

The Reaction Model

The reaction model uses turbulence quantities to calculate a combustion rate. To see if inlet turbulence has an effect on the TITP two calculations were done using baseline inlet conditions (five premixed zones). Increasing the inlet turbulence for 5.5 to 30 percent increased temperatures only slightly near the inlet nozzles. Temperatures further downstream, and for the TITP, were unaffected. Decreasing the inlet turbulence to 0.55 percent also had no effect on the TITP. Modifying the inlet turbulence did not change turbulence levels to a corresponding degree throughout the flow field. Turbulence levels would better have been increased throughout the flowfield by larger differences in inlet velocities, as was done for the diffusion calculation.

As mentioned previously, the reaction model uses two empirical reaction constants. All of the previous calculations shown used $A = 32$ and $B = 4$. These numbers were quoted for a turbulent premixed combustion calculation. Using $A = 4$ and $B = 0.5$ which was quoted for a diffusion combustion calculation did not produce much of a reaction in certain cases. Increasing A and B by a factor of two did not produce realistic temperature profiles either. Figures 10(a) and (b) show the effect of changing the reaction constants for baseline inlet conditions and for inlet conditions with no product. Decreasing A and B by a factor of two (from the high values) did not change the TITP for the baseline inlet conditions but it produced a change in the calculation using no product in the inlet. Further decreasing B for the baseline inlet conditions resulted in a lowering of temperature over most of the TITP. Further decreases in the constants resulted in highly skewed profiles. Using still lower constants resulted in no combustion.

Bounded Skew Upwind Differencing

Finally a calculation was made using the more accurate BSUDS scheme. BSUDS is more computationally expensive and it is more difficult to obtain a converged solution. To see if the BSUDS had an effect on mixing, a calculation was made modeling the inlet as combusted gases. The TITP did differ from a hybrid differencing calculation as shown in figure 11. Whether or not this slight variation in temperature profile is significant to turbine blade life, the calculation indicates the importance of numerical accuracy in making reacting flow calculations. The fact that a change was seen even with the large number of gridpoints used in this calculation, provides strong evidence that fewer gridpoints should not be used in these types of calculations. Routine three-dimensional calculations using such a fine mesh (using perhaps 70 by 55 by 40) are currently impractical from a time and cost standpoint. Less accurate three-dimensional calculations could be performed to establish gross trends in the flow field, but the results should be viewed by a knowledgeable designer who is aware of the numerical inaccuracies.

SUMMARY OF RESULTS

The temperature profiles generated by these calculations with the eddy breakup combustion model show that the turbine inlet temperature profile is affected by changes in the equivalence ratio (if the affected area is large enough), but that other aspects of modeling, such as modeling separate oxygen and hydrogen nozzles or modeling the inlet conditions as hot combustion products or unburned gases has little effect on the final exit temperature profile.

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2. McBride, B.J., and Gordon, S.: Fortran IV Program for Calculation of Thermodynamic Data, NASA TN D-4097, 1967.

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TABLE I.

[$C_p = C_1 + C_2 \cdot T + C_3 \cdot T^2 + C_4 \cdot T^3 + C_5 \cdot T^4$.]

	C_1	C_2	C_3	C_4	C_5
H_2O	4.06123	-8.2329E-4	2.9668E-6	-1.2967E-9	5.5269E-14
O_2	3.57840	-1.1850E-3	4.5000E-6	-3.3454E-9	6.4591E-13
H_2	3.29856	-1.2650E-4	3.2715E-6	-5.7020E-9	2.8912E-12
T > 1000 K					
H_2O	3.42384	1.2083E-3	7.1516E-7	-4.5067E-10	6.6681E-14
O_2	3.67138	5.4117E-4	4.5074E-8	-7.9000E-11	1.5266E-14
H_2	3.27290	1.1923E-4	3.5771E-7	-1.3134E-10	1.4264E-14

TABLE II. - ROCKETDYNE
SUPPLIED INLET
CONDITIONS

Zone	Mass flow, kg/s	
	O_2	H_2
A-B	4.70	4.81
C-D	8.46	7.92
EFG	19.75	17.44
H	7.52	8.27

TABLE III. - INLET CONDITIONS USED FOR BASELINE CALCULATION

[An inlet pressure of 250 atm was used.]

Zone	Mass flow, kg/s		Radial distance, m	Velocity temperature		Equivalence ratio
	O_2	H_2		m/s	K	
IGN	0.209	0.2404	0.0 - 0.0128	77.6	1000.0	9.2
A-B	4.70	4.81	.0128 - .0448	23.9	162.8	8.18
C-D	8.46	7.92	.0448 - .0704	24.7	162.5	7.49
EFG	19.75	17.44	.0704 - .1088	23.3	162.4	7.07
H	7.52	8.27	.1088 - .1216	25.6	162.9	8.79

TABLE IV. - FINAL TEMPERATURE ASSUMING COMPLETE COMBUSTION

Initial temperature	Equivalence ratio	Final temperature
1000.0	Infinity	1000.0
162.8	8.18	1076.2
162.5	7.49	1144.5
162.4	7.07	1191.9
162.9	8.79	1023.4

TABLE V. - INLET CONDITIONS USED FOR DIFFUSION CALCULATION

Zone	Radial distance, m	Velocity, m/s	Mixture	Number of cells
Ignitor	0.0 - 0.0128	77.6	$H_2 + H_2O$	6
A-B	.0128 - .0192	12.08	H_2	2
	.0192 - .0224	48.3	H_2	1
	.0224 - .0288	2.388	O_2	2
	.0288 - .0352	48.30	H_2	2
	.0352 - .0416	2.388	O_2	2
	.0416 - .0448	48.30	H_2	1
C-D	.0448 - .0480	46.95	H_2	1
	.0480 - .0544	2.388	O_2	2
	.0544 - .0608	46.95	H_2	2
	.0608 - .0672	2.388	O_2	2
	.0672 - .0704	46.95	H_2	1
EFG	.0704 - .0768	44.26	H_2	2
	.0768 - .0800	2.388	O_2	1
	.0800 - .0864	44.26	H_2	3
	.0864 - .0928	2.388	O_2	3
	.0928 - .0992	44.26	H_2	4
	.0992 - .1056	2.388	O_2	4
	.1056 - .1088	44.26	H_2	2
H	.1088 - .1120	48.98	H_2	2
	.1120 - .1184	2.123	O_2	4
	.1184 - .1216	48.98	H_2	2

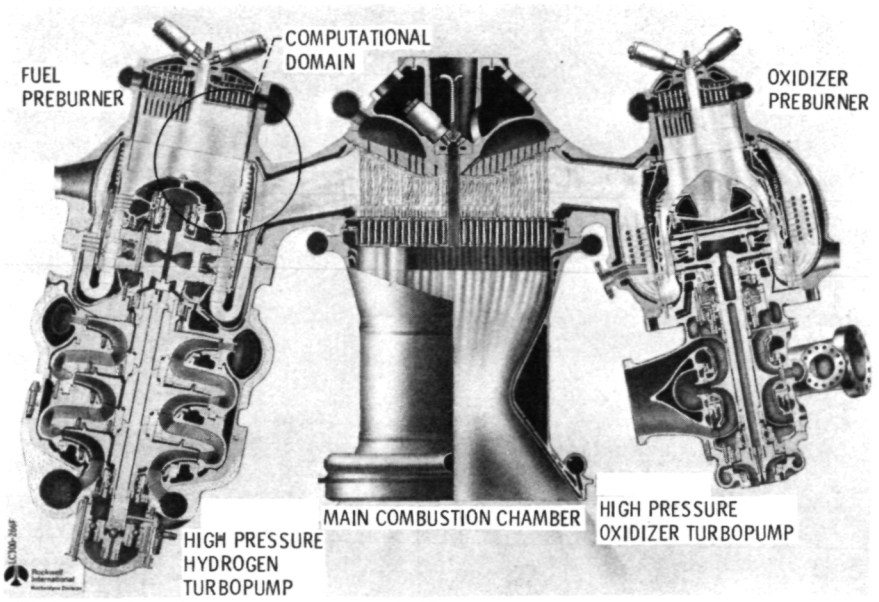
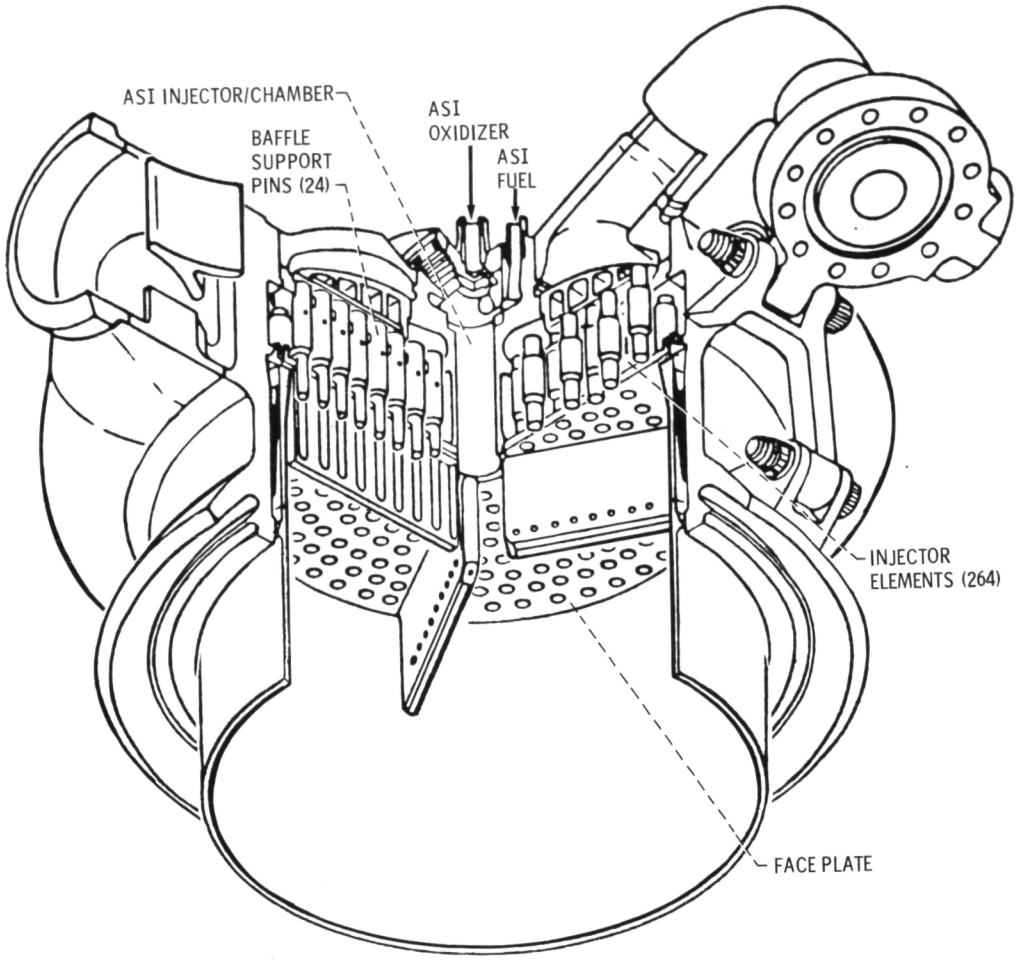


Figure 1. - SSME powerhead component arrangement.



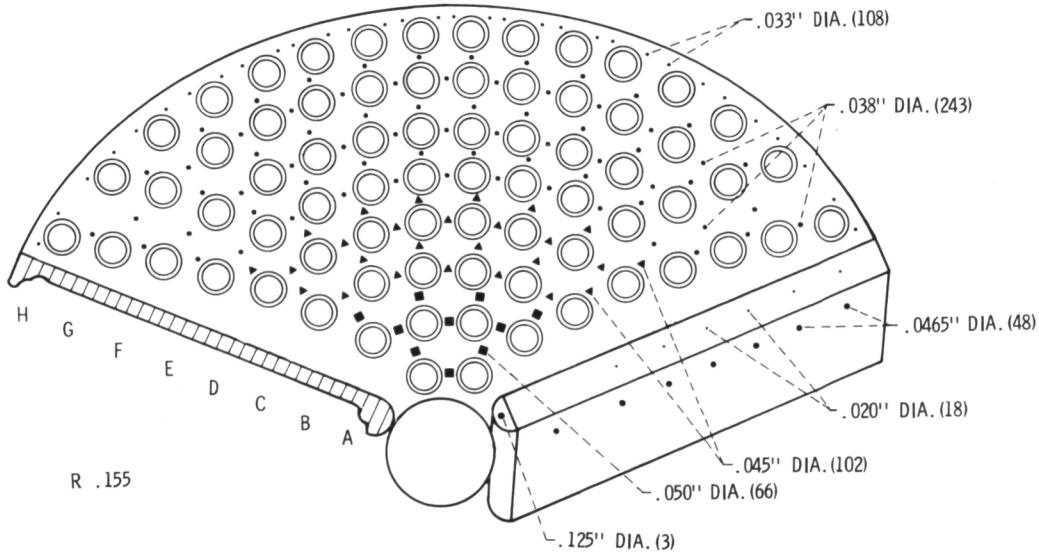


Figure 2. - Schematic of SSME fuel preburner.

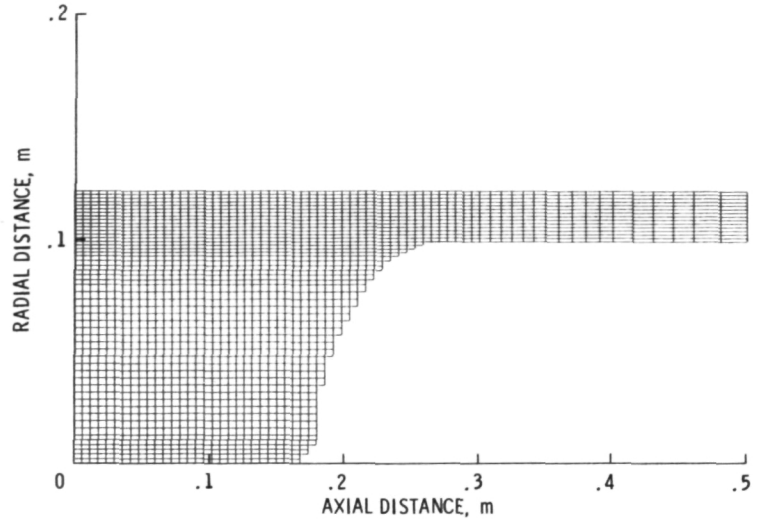


Figure 3. - Computational grid.

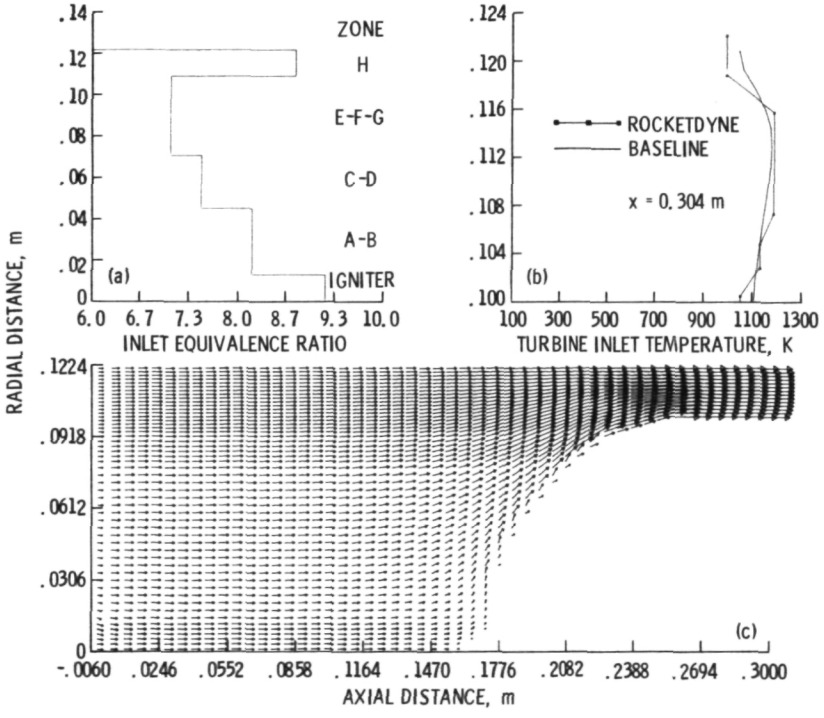


Figure 4. - Baseline calculation using five premixed inlet conditions.

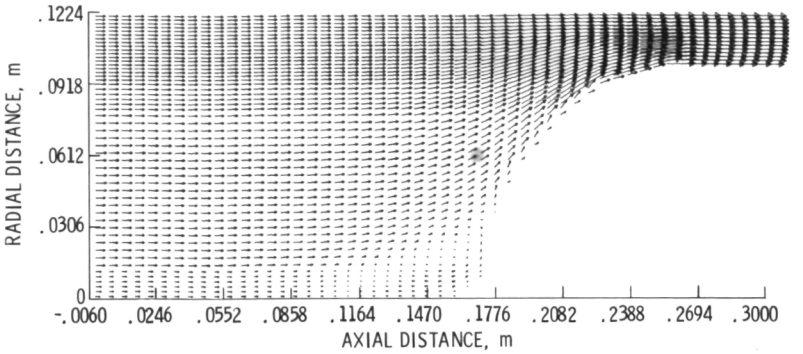


Figure 5. - Velocity vector diagram for hot mixing gas calculation.

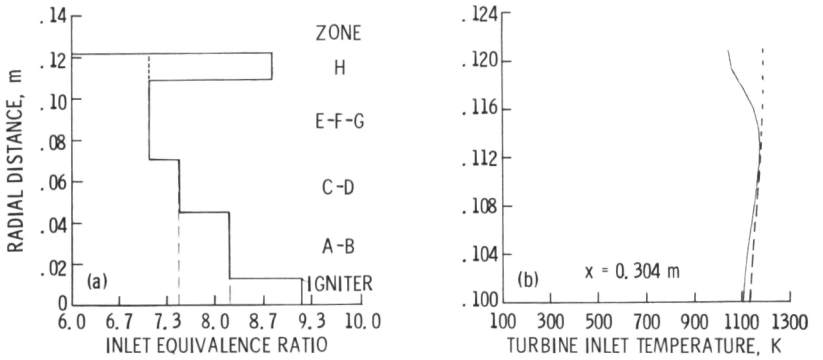


Figure 6

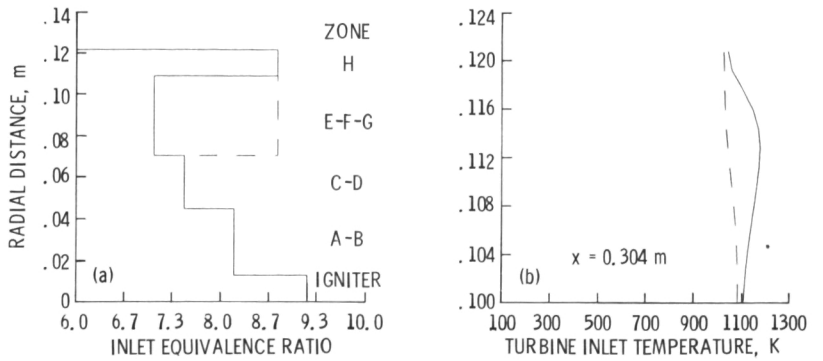


Figure 7

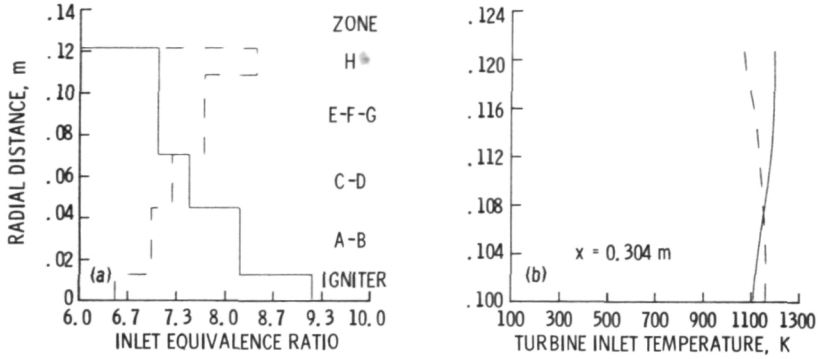


Figure 8

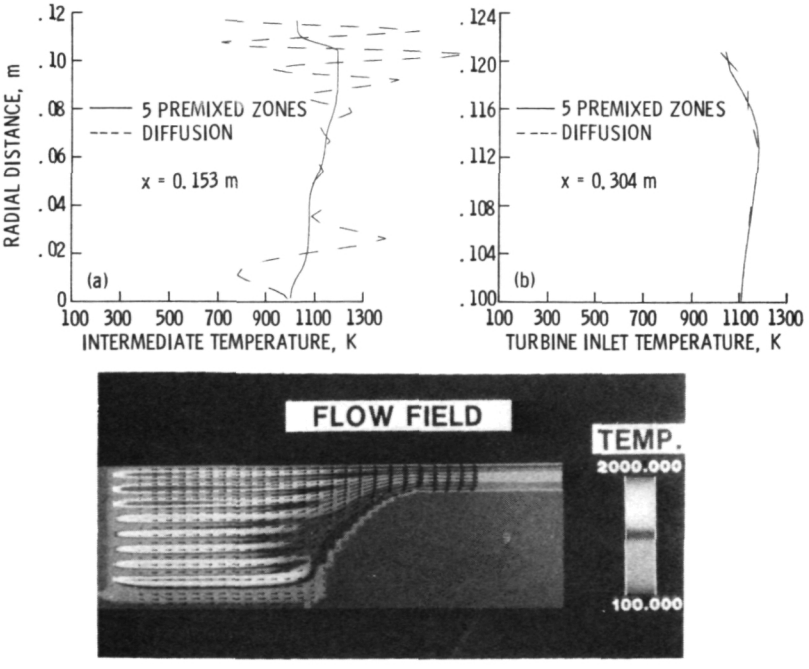


Figure 9. - Diffusion type combustion calculation.

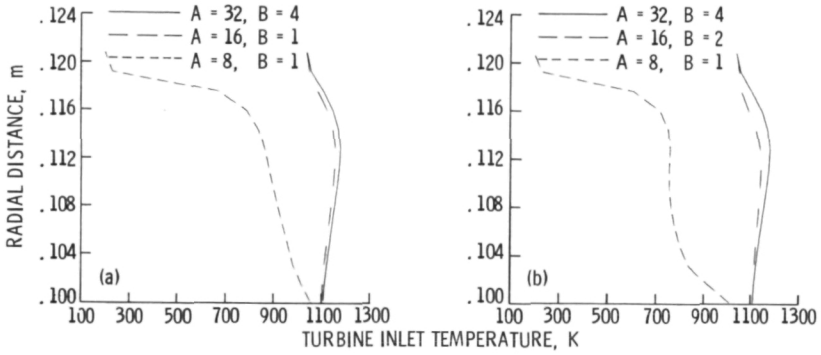


Figure 10. - Different reaction constants.

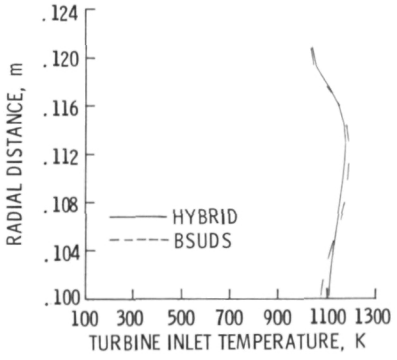


Figure 11

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15. Supplementary Notes

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16. Abstract

The flow field within the fuelside preburner of the Space Shuttle Main Engine is calculated using a reacting flow code (REACT2D). Inlet and modeling parameters involved in the numerical calculation are systematically varied to establish the sensitivity of the calculated exit temperature profile. It is found that differences in the inlet equivalence ratio have a large effect on the turbine inlet temperature profile. A variety of preburner inlet modeling changes such as inlet turbulence level, modeling the gases as burned, unburned, premixed, or unmixed, are shown to have a smaller effect on the calculated turbine inlet temperature profile. Also, the form of finite differencing used is shown to have an effect on the temperature profile.

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